CHAPTER V

RESULTS AND DISCUSSION – Part II

AH-1G ROTOR IN LOW SPEED DESCENT FLIGHT

In the previous chapter, results were given for the UH-60A rotor in a high-speed forward flight condition. In this chapter, results are presented for a two-bladed AH-1G rotor in a low-speed descent condition. In low speed forward flight, compressibility effects and retreating stall phenomena are not of major consequence, somewhat simplifying the analysis. However, numerical modeling of low speed descent conditions introduces two new difficulties. First of all, the effects of the inflow on the blade loads may be very strong, and it is very important to accurately model the wake geometry, and the induced inflow field $\vec{V}_w$. Secondly, oncoming flow may push the vortices up against the rotor disk, giving rise to strong BVI interactions.

5.1 Details of the Flight Test

A flight test of the AH-1G helicopter was performed at NASA Ames Research Center and reported in NASA Reference Publication 1179. This flight test, known as Tip Aerodynamics and Acoustics Test (TAAT) was conducted on an AH-1G Cobra. It used highly instrumented rotor blades and instrumentation hardware developed for the U. S. Army Operational Loads Survey (OLS) test. The available data includes overall thrust coefficient C_T and the C_n at selected sections based integrated pressure data collected from a few pressure ports on the blade.

Several calculations for this helicopter have been reported in literature. Hernandez and Johnson [112] used the full potential rotor code FPR coupled with CAMRAD/JA. Ramchandran et al [20] applied the full potential code HELIX-II, which uses a finite difference method for solving the potential flow over the rotor, in conjunction with the vorticity embedding method for modeling the wake. The lateral and longitudinal cyclic pitch variations were taken from the flight test data, and the coning and first blade flapping harmonics were also obtained from the test for a particular trim condition. Their free wake calculation begins with an initial undistorted wake obtained using a uniform axial flow. Ahmad and Duque [58] used the Navier-Stokes code with the moving embedded grids (Chimera grid). Their solution was impulsively started and became periodic in less than three rotor revolutions. The entire embedded grid system used a total of 1.62 million grid points, requiring roughly 800,000 nodes per blade.

The multi-block H-O grid system is similar to that used in the UH-60A study, with clustering near the tip, near the leading edge and the trailing edge. The grid dimensions are 90x43x80, with 90 chordwise points, 43 points in the spanwise direction, and 80 points in the normal direction. The inner Navier-Stokes zone includes about 39% of the total grid points.

The rotor has a relatively simple geometry. The AH-1G rotor blade uses an OLS (Operational Loads Survey) symmetrical airfoil section and has an aspect ratio of 9.8. The rotor is a two-bladed rectangular planform teetering configuration. The linear twist is -10^0 from root to tip.

The case chosen for detailed validation is at an advance ratio of 0.19, tip Mach number of 0.65, and an overall thrust coefficient of 0.00464. The calculated Reynolds number based on chord is 9.73 million at the tip. The blade loading C_T/σ was 0.0714. The tip path plane angle was -1.87 degrees. Note that the "negative" sign indicates that the rotor disk is tilted aft, representative of a descent condition.

The first blade harmonics (degree) reported by the flight test data are listed below:

θ_0	θ_{1s}	θ_{1c}	β_{1s}	β_{1c}
6.0^0	-5.5^0	1.7^0	-0.15^0	2.13^0

Table 5.1: Blade First Harmonics from Flight Test Data

With this data as an initial guess, an effort was made to trim the collective pitch to match the measured thrust. In order to eliminate the rolling and pitching moment, the lateral and longitudinal cyclic pitch angles were also trimmed manually. The first blade harmonics (degree) after a trimming are given below:

θ_0	θ_{1s}	θ_{1c}	β_{1s}	β_{1c}
8.0^0	-6.5^0	2.5^0	-0.15^0	2.13^0

Table 5.2: Trimmed Blade First Harmonics

5.2 Comparisons with the AH-1G Flight Test Data

Because the effects of the wake geometry are important at the low advance ratios considered, all the calculations were done using the free wake model, unless otherwise indicated. The vortex core was 0.2 chords, and new markers were released every 5 degrees.

Figures 5.1-5.3 show the computed and measured pressure coefficients at several azimuth and radial locations. The solid lines and dashed lines indicate the computed upper and lower surface pressures, respectively. The experimental results are shown in symbols. At the inboard radial station 0.6R, the suction peak on the advancing side compares fairly well with the measurements. But the suction peak at 180^0 azimuthal angle is under-predicted. It may be noted that other numerical simulations, e.g. Ahmad et al [58] had similar discrepancies, although at the 30^0 azimuth and not at the 180^0 azimuth. In the present simulations and in Ahmad's study, the blade torsional deformations were neglected. The flight test data suggests significant torsional deformation may be present, particularly at the inboard stations. Omission of the aeroelastic effects may, at least in part, account for some of the discrepancies.

The surface pressure distributions at outboard radial station 0.91R compare well with the measured data on both the advancing and the retreating side. At the radial station 0.97R very close to the tip, the leading edge surface pressure peaks on the retreating side are consistently under-predicted. The blade vortex interaction may influence the accurate capturing the suction peak. But at all radial stations, an irregular behavior near the leading edge region can be seen, especially between 75^0 and 105^0 azimuthal angle. Ahmad and Duque [58] have also seen similar irregularities and attribute them to errors in the input blade geometry leading edge region.

Figure 5.4 shows the original geometry data for the AH-1G airfoil. It is seen that the original airfoil data (supplied by Strawn et al as part of TURNS sample runs) contains oscillations in the first and second derivatives. The location of the unexplained C_p oscillations in Figure 5.3 directly correlates with the location of the 1st and 2nd derivative oscillations in Figure 5.4. No attempt was made to smooth the airfoil shape because this would adversely affect the one-to-one comparisons with the solutions of Ahmad and Strawn [58], and may also modify blade vortex interaction loads.

Fig.5.5 compares the computed normal load data with the measured airloads at different radial locations (60%, 75%, 91% and 97%). The computed results by hybrid solver using the same first blade harmonics as flight test data without trimming, and using the trimmed cyclic pitching angle are plotted. These results are compared with flight test data. In general, except at the inboard locations, trimming improves the comparison between the computed data and measurements.

Note that the c_n variation has a saw-tooth like oscillation that occurs once every 5^0, particularly in the first and fourth quadrants. This is attributable to the fact that the tip vortex strengths and the associated induced velocity $\vec{V}_w$ are updated only once every 5^0. In forward flight, the rear portion of the rotor disk is affected by the resulting abrupt change in $\vec{V}_w$. This oscillation may be removed by more frequent $\vec{V}_w$ updates, but was not attempted here due to the large CPU time required.

It is seen that the blade loads appear to be discontinuous at $\psi=0^0$ and $\psi=360^0$. This caused by the fact that the tip vortex strengths for the other blades lag the strengths for the reference blade by one full rotor revolution. Only the current blade tip vortex strength is updated every 5 degrees. At $\psi=360^0$, the calculation are stopped, and the tip vortex strengths of the other blades are updated, based on the reference blade $\Gamma_w(\psi)$ history. To assess the long term effects of this lag in update, results for the 2nd and 3rd revolutions are shown at typical radial locations in Figure 5.6. It is seen that the loads repeat themselves from cycle to cycle, except from $\psi>350^0$ and $\psi<45^0$.

Fig. 5.7 shows the trimmed computed results by the hybrid method and the numerical results by Ahmad and Duque [58]. In their calculation, the blade collective was also corrected to match the measured value of thrust. With no further trim, they found that the rotor was approximately balanced in rolling hub moment and out of balance in pitching hub moment, using the same first blade harmonics as the flight test data. Except in the innermost locations, the present simulations tend to agree better with measurements. At the retreating side near 270°, rapid variations due to blade vortex interactions can be seen. The BVI variations are more sharply defined, because the induced velocities are computed using Biot-Savart law. In some instances, a secondary post BVI blip in the computed loads is seen. This secondary peak is also observed in the experiment (e.g. figure 5.7b), but is not nearly as pronounced. The high secondary peaks may have been caused by the fact that the vortex core was held fixed before and after the BVI phenomenon. It is known that the vortex core can substantially increase following a BVI event [6].

5.2.1 Effects of Wake Modeling on Computed Loads

In an effort to assess how the modeling of the rotor wake affects the blade loads, three wake models have been tested:

a) Glauert uniform inflow ($\vec{V}_w$ has a constant magnitude)

b) Prescribed wake model (Wake geometry prescribed using Glauert's theory, $\vec{V}_w$ computed using Biot-Savart law)

c) A free wake model (Wake geometry allowed to deform with time, due to variations in self-induced velocity, $\vec{V}_w$ computed using Biot-Savart law).

The previous results (Figures 5.1-5.7) all correspond to the free wake model.

Figure 5.8 shows the results from the three wake models. As expected, the uniform inflow model can not capture the dynamic loads due to the blade vortex interaction. The free wake and the rigid wake model give comparable result at this advance ratio (μ=0.20). At the inboard region, in the blade vortex interaction region on the retreating side, there are minor differences between loads computed using the free wake model and the prescribed wake. Close to the tip, these two models give nearly identical results. This may be due to the fact that the vortex shed from the reference blade influences the inflow near the tip more than the vortices shed from the other blade.

A limited number of studies were done to assess the effect of the vortex core size, and that of the time interval between the release of tip vortex markers on blade loads. The vortex core size was varied between 0.1 chord and 0.2 chord. The azimuth that the reference blade travels between successive release of the tip vortex markers was varied between 5 degrees and 10 degrees. Every time a new marker is released, the induced velocity field $\vec{V}_w$ was updated.

Figure 5.9 shows the associated blade loads. In general, except during the BVI encounter, the computed loads are insensitive to the vortex core size and the frequency with which vortex markers are released into the wake. This is significant because the fewer the markers in the wake, the lower the computational time will be to compute the Biot-Savart law based induced velocities. In other words, in high advance ratio applications, it should be sufficient to release markers once very 10 degrees or so.

As expected, the vortex core size and the marker release interval play a significant role in the computed loads during the blade-vortex interaction. Very large intervals (e.g. 10 degrees) between the marker release (and the associated abrupt changes in the induced velocity field $\vec{V}_w$) produce larger than expected changes in the loads, as seen in figure 5.9.a. The post-BVI oscillations are also more pronounced when the wake geometry is infrequently updated. It may be concluded that during the BVI phenomenon, the vortex markers should be released more frequently, and the associated velocity field recomputed. It may also be necessary to smooth the induced velocity field between wake updates to smooth the spikes in the velocity that invariably occur at nodes closest to the markers and tip vortex filaments whenever Biot-Savart law is used.

Visualization of the flow field was done in order to understand the differences in the wake geometry computed using the prescribed and free wake models. Consider a typical instance in time when the reference blade is at the azimuthal location $\Psi=270^0$. This location is chosen for the visualization study below because the blade is on the verge of a BVI encounter. Figure 5.10.a shows the perspective view of the wake geometry for the reference blade, computed by the free wake model and the prescribed wake model. Figures 5.10.b, 5.10.c, and 5.10.d give the top and side views, respectively. In these figures, the z-axis is the shaft axis. Figure 5.10.a shows that the edges of the wake tend to roll up, much as in the case of a fixed wing, when a free wake model is used. The vortex roll up, and the associated self-induced distortions are more pronounced on the retreating side than on the advancing side, primarily because of the increased bound circulation, and tip vortex strengths on the retreating side. The top view shown on figure 5.10.b shows that there is very little in-plane distortion of the wake geometry compared to the prescribed wake. That is, the lateral (y-) and the streamwise (x-) distances between the wake markers and the reference blade are quite comparable for the free wake and the prescribed wake. The largest differences between the prescribed wake geometry and the free wake geometry is seen in figure 5.10.c, and in figure 5.10.d. It appears that portions of the free wake do not descend as rapidly as suggested by the prescribed wake model, while others descend more rapidly. The average behavior of the free wake does follow the prescribed wake model. The vertical clearance between the rotor disk (approximated as the x-y plane, z=0) and the wake markers has only a minor effect on the induced velocity. The large horizontal and lateral distances between the wake markers and the reference blade dominate the induced velocity field. This may explain why the prescribed wake model and the free wake model give comparable loads, although their geometries are noticeably different.

Figure 5.11 is a flow visualization reproduced from Ahmad and Duque [58]. This figure from their full Navier-Stokes simulations shows the particle streakline patterns after five rotor revolutions. From a plane of points just behind each blade's tip and root trailing edge, rakes of particles were released every 5 degrees of rotation, and their trajectory followed in time. The side view shown in figure 5.11.b is quite similar to that computed from the present free wake model as shown in figure 5.10.c. Figure 5.11 also shows evidence of excessive wake diffusion, at large distances downstream of the rotor.

The previous Figures 5.10a-d show only the tip vortex from the reference blade. In order to understand how the tip vortex from the second blade is altered, the tip vortex trajectories were computed using free wake and prescribed wake methodologies, and visualized as shown in figures 5.12 and 5.13. These figures correspond to the instance in time when the reference blade is at 270 degrees azimuth. The most notable feature (seen in figure 5.13) is that the tip vortex from the second blade is lifted up due to self-induced velocities, and goes above the rotor disk. This feature is completely missed by the prescribed wake model, which places all the vortex filaments (from the reference blade and the second blade) always underneath the rotor disk.

In spite of the fact that the free wake model and the prescribed wake model give different vertical locations for the tip vortex trajectory for the second blade, they give comparable BVI loads, as observed earlier. It turns out that the BVI phenomena are dominated by the lateral (y-) and streamwise (x-) distances between the vortex and the reference blade, and not by the small vertical clearances. This idea is illustrated in the illustration shown in figure 5.14, for a 2-D parallel BVI situation. In this figure it is clear that the w-component (i.e. downwash) is independent of the sign of z_v value, and will be affected by the larger of the two distances (x_v and z_v).

5.3 AH-1 Scaled Model Studies

Forward flight calculations were also computed for a 1/7 scale model AH-1 rotor experimentally tested by Splettstoesser et al [113]. The rotor has a rectangular planform, and BHT-540 airfoil sections. In order to accommodate pressure instrumentation, the thickness and chord of the OLS airfoil had to be increased in thickness slightly [114], so that the rotor configuration is not identical to that of the AH-1G rotor. The rotor is 1.916m in diameter and has a 10.39cm chord. The linear twist is 8.2 degrees from root to tip and the blade root cutout is at 0.182R.

The validation case for the blade-vortex interaction (BVI) simulation is a low speed descending flight with an advance ratio of 0.164. The tip Mach number is 0.664 and the overall thrust coefficient is 0.0054. The rotor shaft is normal to the freestream direction. The tip path plane angle is -1 degrees. As before, the negative sign indicates that the rotor disk is titled aft, and the oncoming freestream approaches the rotor disk from the bottom side.

The blade dynamics information is taken from a study by Strawn et al [114].

θ_0	θ_{1s}	θ_{1c}	β_{1s}	β_{1c}	$\beta 0$
6.14^o	-1.39^o	0.9^o	0.0^o	-1.0^o	0.5^o

Table 5.3: Blade First Harmonics from Strawn et al [114]

In the calculations by Strawn et al [114], done using the OVERFLOW-CFD solver on an overset grid, the precone angle and the first harmonic flapping were set to be same as the values in the experiment. In their study, the collective and the cyclic pitch angles were adjusted to match the overall measured thrust coefficient and achieve zero pitching and rolling moments of the rotor disk.

The present results computed with the hybrid solver use the same cyclic pitch and flapping angles as in the table above. The collective pitch angle was manually trimmed to match the overall thrust coefficient. Because cyclic pitch angles were not trimmed, the computed rolling and pitching moments are not zero, but are small.

A multi-block H-O grid is used with clustering near the blade tip, leading and trailing edges. The coarse grid system spans 90 degree along the azimuthal direction and consists of 90X43X80 in chordwise, spanwise and normal directions respectively.

The measured and the computed pressure coefficients are compared on the advancing side in Fig.5.15. The experimental data provides the surface pressure measurements only on the rotor upper surface. The computed results show reasonably good agreement with the experimental values. As in the AH-1G rotor study described earlier, an irregular behavior of the surface pressures is seen near the leading edge region on the upper surface. These irregularities are caused by the oscillations in the blade geometry near the leading edge.